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RESEARCH MEMORANDUM

AERODYNAMIC INTERFERENCE EFFECTS ON NORMAL AND

AXIAL FORCE COEFFICIENTS OF SEVERAL

ENGINE-STRUT-BODY CONFIGURATIONS

AT MACH NUMBERS OF 1.8 AND 2.0

By Emil J. Kremzier and Murray Dryer

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NATIONAL ADVISORY COMMITTEE
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RESEARCH MEMORANDUM

AERODYNAMIC INTERFERENCE EFFECTS ON NORMAL AND AXIAL FORCE

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SUMMARY

Aerodynamic interference effects associated with a missile configuration, consisting of a pointed body of revolution with one or two ramjet engines strut-mounted in a vertical plane through the center line of the body, were investigated for several engine locations relative to the body and for a range of angles of attack from 0° to 10° . The investigation was conducted in the Lewis 8- by 6-foot supersonic wind tunnel at Mach numbers of 1.8 and 2.0 and at a test Reynolds number of approximately 28×10^6 based on body length.

The experimental data indicated increases in slope of the normal force curve with outward movement of the engines. Several of the outboard engine positions actually resulted in normal force curve slopes slightly in excess of the sum of the slopes of the normal force curves of the isolated components.

Decreases in axial force occurred with inboard rearward movement of the engines. Maximum decreases in axial forces of the order of 20 to 34 percent less than the sum of the axial forces of the isolated components were noted for the various configurations.

Validity of the theoretical prediction of normal and axial force interference effects was limited to the approximate determination of the trend of these effects with changes in engine location; magnitudes of theoretical and experimental values showed agreement in only a few isolated cases. Body viscous cross-flow separation at higher angles of attack affected the flow field of the upper engine to such an extent that theoretical normal force evaluation was not feasible.

Comparison of lift-drag ratios of various configurations in combination with a hypothetical wing of given lift-drag ratio indicated that the trend of lift-drag ratio of the complete configuration with varying engine position was governed by configuration drag whereas the effect of configuration lift was not noticeable. The trend of lift-drag ratios for the

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various configurations was not necessarily the same as that for the various combinations with a wing; this difference indicated the necessity of considering a complete aircraft in the evaluation of lift-drag ratio.

INTRODUCTION

In the design of supersonic aircraft incorporating a nacelle-body combination, the location of the nacelle with respect to the body has an important influence on aerodynamic interference effects from the standpoint of aircraft lift and drag. Some typical transonic and high subsonic investigations of configurations with nacelle-like bodies of revolution mounted in various positions on sweptback wings are reported in references 1 and 2. The results of these investigations show favorable drag interference effects at zero lift for certain nacelle locations. As part of a general program at the NACA Lewis laboratory to investigate aerodynamic interference effects of nacelle-body combinations at supersonic Mach numbers, reference 3 presents experimental data for various combinations of one and two nacelles (hereinafter called engines) mounted on struts in the vertical plane through the center line of a body. This report is an extension of reference 3 and includes an analysis of the data and a comparison with theory where possible. A brief analysis of the effect of engine location on configuration lift-drag ratio is also included.

Configuration normal and axial force coefficients were determined for various engine locations at Mach numbers of 1.8 and 2.0 through a range of angles of attack from 0° to 10° . The tests were conducted in the Lewis 8- by 6-foot supersonic wind tunnel at a Reynolds number of approximately 28×10^6 based on body length.

APPARATUS AND PROCEDURE

A sketch of the model which consisted of the NACA RM-10 body with two ram-jet engines mounted symmetrically in a vertical plane through the center line of the body is shown in figure 1. Engine location with respect to the body was variable, and the upper or lower engine was separately removable. Force and pressure instrumentation were incorporated as described in reference 3.

Lift and drag coefficients of reference 3 were reduced to normal and axial force coefficients for purposes of analysis. All force coefficients were obtained from forebody forces only; that is, the base pressures were corrected to free-stream static pressure, and the internal aerodynamic forces were excluded from the balance readings. Normal

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forces resulting from the turning of the internal stream tube in the vicinity of the engine inlet are included in the normal force coefficients, inasmuch as they are essentially independent of engine internal flow conditions for supercritical flow.

RESULTS AND DISCUSSION

Characteristics of Isolated Components

In order to evaluate the effects of aerodynamic interference on the body-engine configuration, the characteristics of the isolated components must be considered.

Isolated body. - Normal and axial force coefficients for the isolated body have been plotted as a function of angle of attack for Mach numbers of 1.8 and 2.0 in figure 2. Theoretical normal force coefficients were calculated by the method of reference 4. Theoretical axial force coefficients at zero angle of attack were obtained from the sum of the pressure drag given by linearized potential theory and the friction drag calculated from the equation for turbulent incompressible flow over a smooth flat plate (reference 5). The axial friction force coefficient was assumed constant with angle of attack, and the axial pressure force coefficient was assumed to vary in accordance with linearized potential theory. As observed from the figure, reasonably good agreement is noted between experiment and theory. Furthermore, these data, which were presented in terms of lift and drag coefficient curves in reference 3, substantiate reasonably well the results presented in references 6 and 7 for previous investigations of the RM-10 body.

Isolated engine. - Normal and axial force coefficients are presented in figure 3 for the same range of variables covered with the isolated body. Theoretical curves for the external normal force coefficient were obtained by the method of reference 4, the equations of which were modified to apply to an open-nosed body. Internal normal force was evaluated from the momentum change in a full-inlet stream tube in turning from the free-stream to the axial direction of the engine. The theoretical axial force coefficients at zero angle of attack were obtained from the sum of the theoretical friction drag (based on two-dimensional compressible flow theory of reference 8) and the theoretical pressure drag calculated from the curves presented in reference 9. Friction forces were then assumed constant with angle of attack and pressure forces were again assumed to vary in accordance with linearized potential theory. A comparison between the experimental and the theoretical curves indicates reasonably good agreement.

Normal force curves for both the isolated body and the isolated engine exhibit continuously increasing slopes with increasing angle of

attack. The variation of axial force coefficient with angle of attack is quite small for both body and engine.

Characteristics of Representative Configurations

The trends of the curves for normal and axial force coefficients as a function of angle of attack are similar for most configurations investigated. These trends are illustrated in figure 4 for a representative configuration (B-2B) at Mach numbers of 1.8 and 2.0. As noted for the isolated body and isolated engine, the normal force curve slope exhibits a gradual continuous increase with increasing angle of attack, whereas the variation in axial force throughout the angle of attack range is quite small. Several of the configurations (upper engine) have slight decreases in their normal force curve slopes at the higher angles of attack as a direct result of the loss in normal force of the upper engine as it becomes immersed in the body wake. A more detailed discussion of these phenomena will be presented later.

Interference Effects

Experimental normal force. - In order that some concept as to the magnitude and trend of the effects of aerodynamic interference on the normal force curve slope may be acquired, a normal force interference factor i_N is defined as

$$i_N = \frac{\left[\frac{dC_N}{d\alpha} \right]_c - \left[\left(\frac{dC_N}{d\alpha} \right)_b + \left(\frac{dC_N}{d\alpha} \right)_e \right]}{\left[\left(\frac{dC_N}{d\alpha} \right)_b + \left(\frac{dC_N}{d\alpha} \right)_e \right]}$$

(All symbols are defined in appendix A.) A negative interference factor therefore indicates that the slope of the curve for the complete configuration is less than the sum of the slopes of its individual components. For convenience, interference factors i_N were evaluated at angles of attack of 2° and 8° so that representative interference effects in the lower ($0^\circ < \alpha < 4^\circ$) and higher ($6^\circ < \alpha < 10^\circ$) ranges of angles of attack could be compared. The use of normal force curve slopes in the evaluation of i_N gives qualitative comparisons of normal force interference effects; however, actual values of the normal force coefficient at angles of attack of 2° and 8° are tabulated in table I, together with the corresponding slope of the curve at these angles for the various configurations investigated. In calculating the interference factor, normal force effects due to the presence of the engine mounting struts were neglected, inasmuch as these effects are believed to be quite small.

The solid lines of figures 5 and 6 are experimental contour plots of lines of constant normal force interference factor at angles of attack of 2° and 8° , respectively, for the range of engine locations investigated. Coordinates of the curves represent the position of intersection of the engine and strut center lines in the vertical plane relative to the body.

In general, the interference factor varied from some negative value to a slight positive value with outward movement of the engines and, in the absence of the strut, would return to a zero value when the respective body and engine flow fields become sufficiently separated that no interaction occurred, but the range of engine locations investigated was generally insufficient to verify this condition. A general trend of less negative interference factor with rearward movement of the engines was also noted on most of the plots, although this variation was small compared with that observed with outward movement of the engines. No significant Mach number effects were noted.

Variation of $(i_N)_{\alpha=2^\circ}$ with lateral movement of the engines was greater for twin-engine configurations than that for upper- or lower-engine configurations (fig. 5). For example, $(i_N)_{\alpha=2^\circ}$ for twin-engine configurations at a Mach number of 2.0 varied from -0.25 for inboard engine positions to +0.05 for outboard positions; whereas normal force curve slopes for inboard engine positions of single-engine configurations exhibited a maximum reduction of 10 to 15 percent of that for the sum of the slopes of the isolated components. If the negative interference factors of lower- and upper-engine configurations are added algebraically, approximate results for twin-engine configurations are obtained; this result indicates negligible engine-engine interference effects. Interference of body wake on the engines or engine wake on the body is small at low angles of attack, as evidenced by the quantitative similarity of the family of curves in figures 5(b) and 5(c) for lower- and upper-engine configurations, respectively.

Contours of the normal force interference factor $(i_N)_{\alpha=8^\circ}$ are shown in figure 6. A comparison of the slopes of the contours for the twin-engine and upper-engine configurations at angles of attack of 2° (figs. 5(a) and 5(c)) and 8° (figs. 6(a) and 6(c)) shows a reversal in the trend of i_N with longitudinal engine movement. The variation of $(i_N)_{\alpha=8^\circ}$ with lateral engine movement is also much greater than that of $(i_N)_{\alpha=2^\circ}$ for these configurations. This reversal in trend of the $(i_N)_{\alpha=8^\circ}$ curves, together with the rather severe reduction in normal force curve slope for inboard engine positions, is believed to be caused by the immersion of the upper engine in the cross-flow vortex field of the body, the formation of which occurs at the higher angles of attack as discussed in references 10 and 11. The energy losses associated with this vortex field cause reductions in the normal force contribution of

the upper engine. The magnitude of this reduction will, of course, be dependent upon the extent of immersion of the engine in the vortex field. On the other hand, $(i_N)_{\alpha=8^\circ}$ contours for the lower-engine configurations (fig. 6(b)) show variations that are comparable ($(i_N)_{\alpha=8^\circ} = -0.10$ for inboard forward engine positions) to those noted at an angle of attack of 2° (fig. 5(b)); similar negligible engine wake effects are thus indicated. For example, the configuration with the upper engine mounted in the rearmost inboard position has a normal force curve slope about 50 percent of that of the sum of the slopes of the individual components, whereas the analogous lower-engine position shows no interference. It is concluded that even for high angles of attack of the order of 8° , lower-engine configurations will experience normal force interference effects due primarily to downwash or upwash rather than to viscous effects experienced by upper- and twin-engine configurations.

Theoretical normal force. - An attempt was made to predict theoretically the slopes of the normal force curves for the various configurations. Accordingly, interference lift curve slopes for the various body-engine combinations were calculated by the method presented in reference 12 and outlined in appendix B.

In order to compare the results of this theoretical treatment of the lift curve slope with experimental values, theoretical lift interference factors were calculated in the same manner as the normal force interference factor $(i_N)_{\alpha=2^\circ}$ for the various configurations. The theoretical lifts (considered now as normal force at the low angles of attack) per unit angle of attack for the separate components were calculated by means of slender-body theory. The results are shown as dashed line contours in figure 5 for twin-, lower-, and upper-engine configurations, respectively, at Mach numbers of 1.8 and 2.0.

Theoretical and experimental curves of figure 5 both show the same qualitative trends and magnitudes. The magnitudes, however, are seldom exactly the same. The theoretical treatment may therefore be used as a guide for predicting the trend of the normal force interference effects as well as approximate magnitudes for various engine locations. When the distance between body and engine center lines is of the same order of magnitude as the body or engine diameter, the slender-body approximation is less valid and results in misleading interference factors. This phenomenon is especially marked in the vicinity of forward engine locations (fig. 5). In any event, these discrepancies indicate the need for further refinements in the theoretical treatment.

At the higher angles of attack where linearized theory does not account for the viscous cross forces on the body and one or both engines, an average value of the body upwash calculated by the method of reference 12 was arbitrarily used. This upwash provided a constant cross flow

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Experimental axial force. - The variation in axial force coefficient with angle of attack was quite small for all configurations investigated, as previously illustrated in figure 4. Consequently, only zero angle of attack data will be used in discussing axial force interference effects. Accordingly, an axial force interference factor is defined as

$$i_A = \frac{C_{A,c} - (C_{A,b} + C_{A,e} + C_{A,s})}{C_{A,b} + C_{A,e} + C_{A,s}}$$

A negative value of i_A indicates that the axial force of the complete configuration is less than the sum of the axial forces of the isolated components. Strut axial force coefficient was obtained by multiplying the exposed strut length by the axial force coefficient per unit length based on maximum body diameter. The drag of the body with two struts for two different strut lengths as presented in reference 3 was used to determine the strut drag per unit length ($C_{A,s}$) at zero angle of attack (same as axial force). Values of $C_{A,s}$ of 0.00450 per inch at M_0 of 1.8 and 0.00464 per inch at M_0 of 2.0 were obtained.

Contour plots of experimental axial force interference factor are presented in figure 7 (solid lines). Single-engine configuration plots were obtained from average faired values of axial force coefficients of corresponding upper- and lower-engine configurations since their axial forces are actually the same. In general, the interference factor varies

from approximately zero for the foremost position of the engines to some negative value for the rearmost inboard engine position. Figure 7 indicates that these negative values of i_A result in reductions of axial force coefficient of from 20 to 34 percent of that obtained from the sum of the axial forces of the individual components. For twin-engine configurations, i_A decreases with increasing strut length to some minimum value and then increases again for forward engine positions. Thus, it is possible to have two different strut lengths exhibiting the same value of i_A in one of these given forward longitudinal engine positions.

Theoretical axial force. - Theoretical interaction drag for the various configurations investigated may be determined by the method of reference 13. This interaction drag evaluated in the form of an axial force interference factor contour plot for single- and twin-engine configurations at Mach numbers of 1.8 and 2.0 is presented in figure 7 (dashed lines). A considerable difference in magnitude between experiment and theory is observed particularly for the inboard engine positions. For example, an experimental drag decrease of about 25 percent is noted for configuration C-1B (fig. 7(a), $M_0 = 1.8$), whereas theory predicts a value of about 15 percent. These differences probably arise largely from inaccuracies associated with slender-body theory when the distance between center lines of interacting bodies becomes small in comparison with the body diameters. Furthermore, the body interference drag that results from the flow field generated by the engine due to presence of the body was neglected. The radial velocity component, which was not considered in the theory, may also have some effect on the drag when the engines are mounted close to the body. Strut interaction drag is another factor that was not considered in the theoretical calculations, although the contribution of the struts is probably negligible for the shorter strut lengths. It is evident, therefore, that a more exact theoretical treatment is desirable. Despite the differences in magnitude noted between the experimental and theoretical values of i_A , the theoretical values indicate the greatest decrease in drag with the engines in the rearmost inboard position. This decrease has also been noted experimentally and a value less than the sum of the axial forces of the isolated components results directly from location of the engines in a region of favorable buoyancy.

Base pressure. - Body base-pressure coefficient as a function of engine-body center-line distance for the various configurations investigated is presented in figure 8 for zero angle of attack and Mach numbers of 1.8 and 2.0. The dashed lines give values of base-pressure coefficient for the isolated body.

Inconsistencies are noted in the trends of the coefficients with increasing strut length as shown in figure 8 for the various configurations. However, the base pressures generally are more negative for the body-engine configurations at zero angle of attack than for the isolated

body. Reference 6 also shows decreases in base pressure for the RM-10 body with the additional stabilizing fins, indicating that the addition of external appendages causes increases in base drag for all cases cited.

Effect of Engine Location on Lift-Drag Ratio

In order to evaluate the significance of the trend of configuration lift-drag ratios with engine position, comparison of the configuration lift-drag ratio $(L/D)_c$ with a composite aircraft lift-drag ratio (configuration with wing) $(L/D)_t$ will be made. Since the range of angles of attack covered in the present investigation did not permit attainment of peak configuration lift-drag ratios (reference 3), variation of $(L/D)_c$ and $(L/D)_t$ with engine movement at a fixed angle of attack will be considered.

Contour plots of lines of constant $(L/D)_c$ for various engine locations are shown in figures 9(a), 10(a), and 11(a) for twin-engine, lower-engine, and upper-engine configurations, respectively, at M_0 of 2.0 and α_c of 8° . The contours are governed by the relative variations of the lift and drag coefficients for various engine locations, and no consistent trends among the three figures are noted.

If a composite aircraft is simulated by combining the preceding configurations with a wing of given lift-drag ratio and neglecting the interference effects between the wing and the engine-body combination, the aircraft lift-drag ratio becomes

$$(L/D)_t = \frac{\frac{W_G}{q_0 S_m}}{(C_D)_c + \frac{\frac{W_G}{q_0 S_m} - (C_L)_c - C_T \sin \alpha}{(L/D)_w}} \quad (1)$$

Figures 9(b), 10(b), and 11(b) present contour plots of $(L/D)_t$ utilizing a wing combined with the twin-engine, lower-engine, and upper-engine configurations, respectively. The following assumptions were made:

$W_G = 50,000$ pounds	$q_0 = 678$ pounds per square foot
Altitude = 50,000 feet	$(L/D)_w = 5.0$
$S_m = 27.2$ feet	$\alpha_c = 8^\circ$
$M_0 = 2.0$	NACA standard atmosphere

The value of $(L/D)_t$ for all configurations is seen to increase with rearward inboard engine movement. The decrease of configuration drag with inward, rearward positioning of the engines (reference 3) supports the trend of the $(L/D)_t$ contours. Decreases of configuration lift with inward movement of the engines oppose this trend. It may be concluded, therefore, that configuration drag governs the trend of the aircraft $(L/D)_t$, whereas configuration lift is only of secondary importance. Thus, a comparison of the configuration alone with the configuration with wing curves (figs. 9 to 11) shows that the addition of a wing may change the lift-drag ratio characteristics of engine-body combinations. In any event, the complete aircraft should be considered when an engine-body configuration is chosen as part of the aircraft.

SUMMARY OF RESULTS

Engine-strut-body configurations with the engines mounted in a vertical plane through the center line of the body were investigated in the Lewis 8- by 6-foot supersonic wind tunnel at Mach numbers of 1.8 and 2.0. The following interference effects on the aerodynamic characteristics were noted:

1. For twin-engine (above and below body) as well as single-engine (above body or below body) configurations with the engines located close to the body, the normal force curve slopes were about 10 to 25 percent less than the sum of the slopes for the individual components at $0^\circ < \alpha < 4^\circ$, where α is the angle of attack. As one or both engines were moved outward and rearward, this interference effect diminished to zero and then became slightly favorable. The adverse interference was greater for twin-engine configurations because of apparent additive interference from each engine. Theoretical calculations may be used as a guide for predicting the above trend of normal force curve slopes, although quantitative agreement was only fair.

2. For single-engine configurations at $6^\circ < \alpha < 10^\circ$ with the engines mounted under the body in forward inboard positions, normal force curve slopes were about 10 percent less than the sum of the slopes for the individual components. Rearward engine movement resulted in interference effects similar to those experienced at $0^\circ < \alpha < 4^\circ$, which indicated negligible viscous interference. For twin-engine and single-engine above-body configurations at $6^\circ < \alpha < 10^\circ$, normal force curve slopes were reduced as much as 50 percent of the sum of the slopes for the individual components for inboard engine positions because of immersion of the upper engine in the body vortex field. Outward movement of one or both engines resulted in diminishing viscous effects until negligible interference occurred.

Theoretical calculations, which included a small viscous approximation, failed to predict the viscous interference for twin- and upper-engine configurations because of the large effect of the viscous cross flow on the upper engine. The calculation, however, partly verified the dominant role of interference upwash for lower engine configurations.

3. Decreases in axial force coefficient for all configurations with inboard rearward movement of the engines were noted. For the various configurations, decreases in axial force of the order of 20 to 34 percent less than the sum of the axial forces of the isolated components were obtained. Theoretical calculations also indicated that the greatest decreases in axial force were obtained with the engines in the rearmost inboard position, although quantitative agreement between experiment and theory was poor.

4. Calculations of lift-drag ratio of a composite aircraft utilizing the configurations investigated indicated that configuration drag played a dominant role in determining the magnitude of the lift-drag ratio whereas configuration lift was only of secondary importance. Comparison of the trend of the lift-drag ratios of the configurations with a hypothetical wing and the lift-drag ratios of the configurations alone with varying engine location showed that the lift-drag ratios of the configurations alone may or may not have the same trend as the complete configurations. The complete aircraft should thus be considered when lift-drag ratio is evaluated.

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APPENDIX A

SYMBOLS

A	external forebody axial force
A_{cyl}	cross-sectional area of hypothetical cylinder whose volume is equal to portion of body between $1b$ and $2b$ and whose length is equal to $(2b - 1b)$
b	local body radius
C_A	axial forebody force coefficient, $A/q_0 S_m$
C_D	external forebody drag coefficient, $D/q_0 S_m$
C_L	lift coefficient, $L/q_0 S_m$
C_N	normal force coefficient, $N/q_0 S_m$
C_{p_b}	base-pressure coefficient, $(p_b - p_0)/q_0$
C_T	internal thrust coefficient, $F/q_0 S_m$
D	external forebody drag
F	internal thrust
i_A	axial force interference factor, $\frac{C_{A,c} - (C_{A,b} + C_{A,e} + C_{A,s})}{C_{A,b} + C_{A,e} + C_{A,s}}$
i_N	normal force interference factor, $\frac{\left[\frac{dC_N}{d\alpha}\right]_c - \left[\left(\frac{dC_N}{d\alpha}\right)_b + \left(\frac{dC_N}{d\alpha}\right)_e\right]}{\left[\left(\frac{dC_N}{d\alpha}\right)_b + \left(\frac{dC_N}{d\alpha}\right)_e\right]}$
L	lift
$(L/D)_c$	lift-drag ratio of engine-body configuration
$(L/D)_t$	lift-drag ratio of combined wing and engine-body configuration
$(L/D)_w$	lift-drag ratio of wing
M_0	free-stream Mach number
N	normal force

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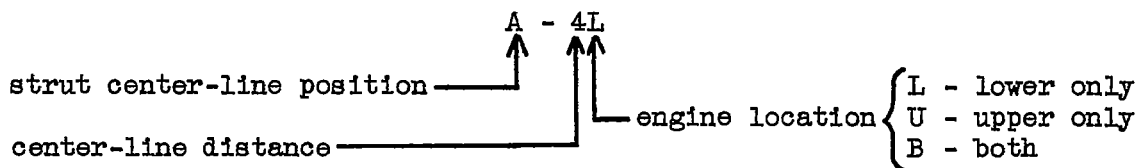
P_b	base pressure
P_0	free-stream static pressure
q_0	free-stream dynamic pressure
S	cross-sectional area
S_{cyl}	cross-sectional area of hypothetical cylinder whose volume is equal to portion of engine between $1e$ and $2e$ and whose length is equal to $(2e - 1e)$
S_m	maximum cross-sectional area of body
u	perturbation velocity in x-direction
U	free-stream velocity
w	perturbation velocity in z-direction
W_G	gross weight
x	longitudinal distance from body nose to local station along body
z	distance between body and engine center lines in the vertical plane
α	angle of attack, deg
$\frac{dC_N}{d\alpha}$	slope of normal force curve

Subscripts:

b	isolated body
B, b	base of body
B, e	base of engine
c	body-engine configuration
e	isolated engine
I, e	inlet of engine
s	isolated strut

- 1e station at engine inlet
- 2e station at intersection of engine center line and Mach line
from body base
- 1b station at intersection of body center line and Mach line
from engine lip
- 2b station at intersection of body center line and Mach line
originating from corner of engine base

Configuration designation (fig. 1):



APPENDIX B

EQUATIONS FOR THE DETERMINATION OF INTERFERENCE LIFT

As noted in reference 12, the following assumptions are made:

- (1) The linearized differential equation of motion is assumed to apply.
- (2) The lifts of the individual components are determined using slender-body theory.
- (3) The upwash is constant around each body and is considered equal to its value at the center line.
- (4) The contribution of the sidewash is negligible, assuming constant sidewash around each body.

Assumption (1) leads to the relation of the pressures to the upwash field by the condition of irrotationality,

$$\frac{\partial u}{\partial z} = \frac{\partial w}{\partial x} \quad (B1)$$

From assumption (2) the lift coefficient of the body alone is the same as that resulting from an incompressible flow about an inclined body, namely,

$$C_{L,b} = 2\alpha \frac{S_{B,b}}{S_m} \quad (B2)$$

where α is given in radians.

It should be pointed out that flow separation actually prevents the development of negative lift downstream of the body maximum cross-sectional area. Equation (B2) does not take into consideration this phenomenon and results in lift curve slopes which are about half those obtained experimentally, as noted in the following table:

α	M_0	Experiment		Theory	
		$\left(\frac{dC_N}{d\alpha}\right)_b$	$\left(\frac{dC_N}{d\alpha}\right)_e$	$\left(\frac{C_L}{\alpha}\right)_b$	$\left(\frac{C_L}{\alpha}\right)_e$
2°	1.8	0.0275	0.0280	0.0129	0.0244
2°	2.0	.0292	.0280	.0129	.0244
8°	1.8	0.0748	0.0453	0.0370	0.0327
8°	2.0	.0855	.0445	.0370	.0327

If S_m were used instead of $S_{B,b}$ (assuming complete separation at S_m), better "theoretical" results would be obtained; however, it is desirable for these purposes to maintain slender-body theory in order to establish a datum upon which further approximations (for example, possible separation at S_m) may later be made.

The lift coefficient of the engine alone is similarly written as the sum of the internal and external (slender-body theory) lifts, namely,

$$C_{L,e} = 2\alpha \left(\frac{S_{B,e} + S_{I,e}}{S_m} \right) \quad (B3)$$

When the engine is located within the flow field of the body, the upwash due to the body and felt by the engine may be taken as a first approximation along the center line of the engine according to assumption (3). The resulting total external lift coefficient for the engine is then

$$\left(C_{L,e} \right)_{\text{external}} = 2\alpha \left(\frac{w_{B,e}}{U\alpha} + 1 \right) \frac{S_{B,e}}{S_m} \quad (B4)$$

The internal lift coefficient (assuming supercritical flow through the inlet) from momentum considerations is

$$\left(C_{L,e} \right)_{\text{internal}} = 2\alpha \left(\frac{w_{I,e}}{U\alpha} + 1 \right) \frac{S_{I,e}}{S_m} \quad (B5)$$

Since each engine is immersed in an upwash field which varies laterally, a further correction is made to account for the resulting buoyancy effects. As shown in reference 12, the resulting buoyant lift coefficient for the engine is

$$\left(C_{L,e} \right)_{\text{buoyant}} = 2\alpha \left[\frac{w_{1e}}{U\alpha} - \frac{w_{2e}}{U\alpha} \right] \frac{S_{cyl}}{S_m} \quad (B6)$$

Similarly, the buoyant lift coefficient for the body due to the upwash generated by the engine is

$$\left(C_{L,b} \right)_{\text{buoyant}} = 2\alpha \left[\frac{w_{1b}}{U\alpha} - \frac{w_{2b}}{U\alpha} \right] \frac{A_{cyl}}{S_m} \quad (B7)$$

Aside from the buoyant forces considered previously, no consideration is made regarding the lift of the body that results from the upwash generated by the engine due to the presence of the body.

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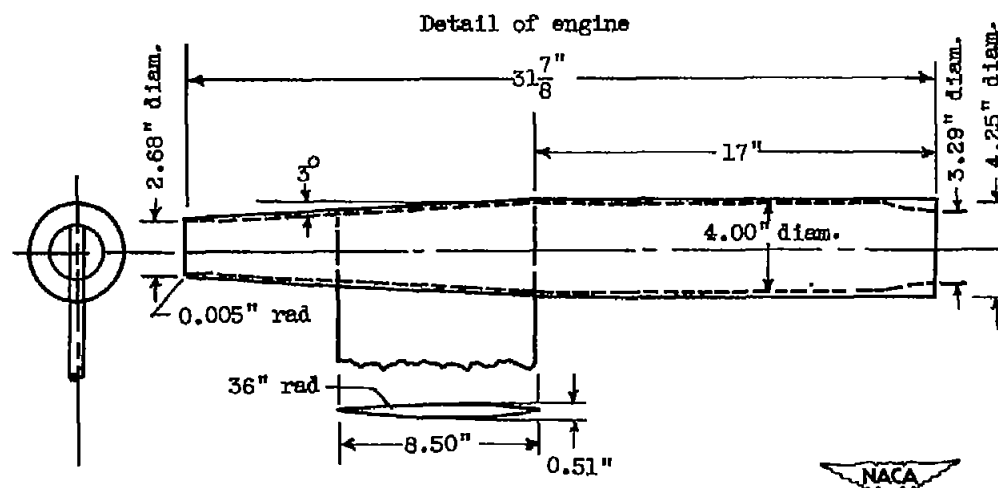
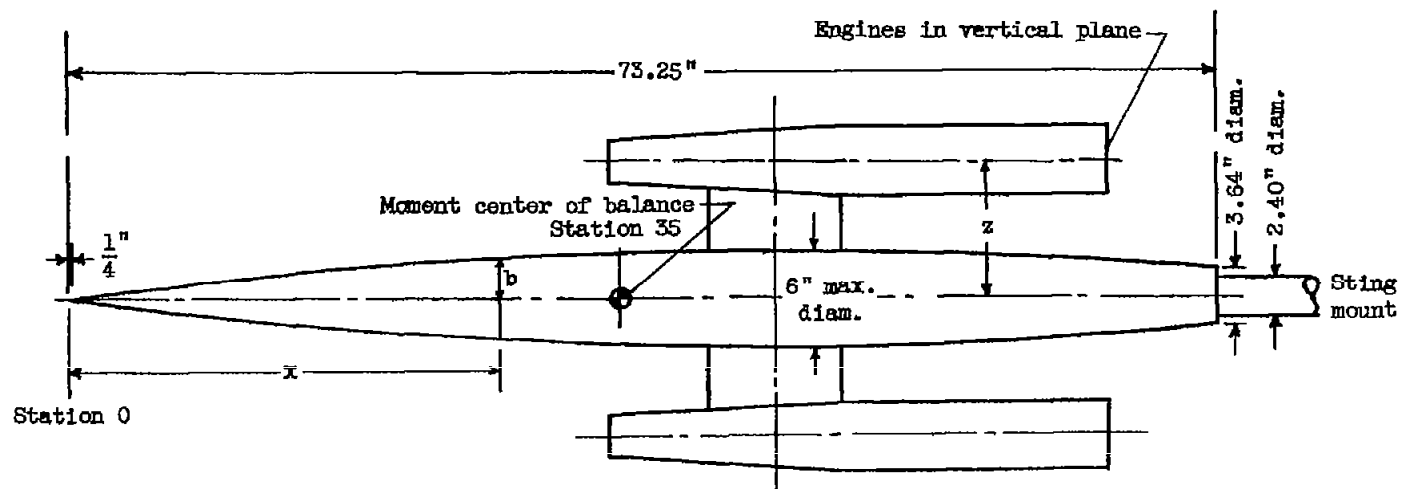
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TABLE I - SUMMARY OF EXPERIMENTAL FORCE CHARACTERISTICS FOR VARIOUS CONFIGURATIONS

Configu- ration	Mach number, M_0											
	1.8						2.0					
	Angle of attack, α (deg)											
	0	2	8	2	8	0	0	2	8	2	8	0
	Normal force coefficient C_N			Slope of normal force curve $\frac{dC_N}{d\alpha}$		Axial force coeffi- cient C_A	Normal force coefficient C_N			Slope of normal force curve $\frac{dC_N}{d\alpha}$		Axial force coeffi- cient C_A
Body	0	0.049	0.322	0.0275	0.0748	0.113	0	0.047	0.350	0.0292	0.0855	0.113
Engine	0	.057	.269	.0280	.0453	.039	0	.063	.287	.0280	.0445	.035
A-1B	0	.140	.593	.0619	.0986	.208	0	.136	.619	.0644	.1114	.185
A-2B	0	.120	.622	.0604	.1334	.197	0	.130	.670	.0652	.1519	.176
A-3B	0	.117	.692	.0678	.1536	.219	0	.138	.761	.0761	.1689	.191
A-4B	0	.152	.765	.0772	.1576	.242	0	.176	.807	.0824	.1865	.224
B-1B	0	.143	.602	.0627	.0921	.160	0	.126	.639	.0624	.1017	.143
B-2B	0	.133	.686	.0685	.1227	.170	0	.132	.721	.0699	.1386	.151
B-3B	0	.129	.741	.0713	.1628	.207	0	.145	.822	.0793	.1778	.175
B-4B	0	.170	.826	.0834	.1671	.230	0	.177	.884	.0907	.1780	.215
C-1B	0	.123	.566	.0625	.0857	.149	0	.132	.603	.0684	.0999	.126
C-2B	0	.134	.679	.0672	.1148	.165	0	.140	.732	.0730	.1314	.137
C-3B	0	.150	.789	.0760	.1627	.185	0	.162	.884	.0822	.1712	.157
C-4B	0	.147	.836	.0821	.1621	.210	0	.164	.891	.0906	.1682	.188
A-1L	0.017	0.110	0.513	0.0485	0.1027	0.147	0.020	0.118	0.535	0.0509	0.1121	0.134
A-2L	.017	.109	.537	.0499	.1114	.152	.032	.127	.580	.0518	.1220	.145
A-3L	.034	.129	.561	.0509	.1127	.168	.036	.141	.611	.0543	.1229	.145
A-4L	.021	.125	.578	.0533	.1151	.181	.024	.122	.619	.0570	.1236	.168
B-1L	.065	.161	.602	.0505	.1163	.142	.074	.165	.635	.0511	.1261	.128
B-2L	.057	.162	.612	.0526	.1187	.145	.062	.171	.661	.0570	.1270	.128
B-3L	.042	.145	.607	.0549	.1217	.155	.052	.156	.655	.0568	.1294	.140
B-4L	.043	.150	.632	.0589	.1202	.163	.043	.160	.668	.0588	.1287	.164
C-1L	.013	.106	.544	.0503	.1202	.135	.020	.122	.606	.0551	.1316	.113
C-2L	.008	.111	.586	.0519	.1284	.130	.016	.127	.649	.0596	.1345	.113
C-3L	.021	.120	.615	.0560	.1265	.155	.040	.151	.691	.0619	.1346	.134
C-4L	.039	.153	.650	.0601	.1216	.162	.049	.167	.704	.0632	.1304	.130
A-1U	-0.032	0.055	0.403	0.0456	0.0817	0.148	-0.032	0.049	0.438	0.0475	0.0927	0.133
A-2U	-.025	.066	.465	.0471	.1059	.160	-.044	.055	.488	.0500	.1210	.133
A-3U	-.046	.054	.482	.0506	.1207	.165	-.044	.060	.523	.0530	.1354	.140
A-4U	-.025	.077	.513	.0522	.1221	.178	-.027	.070	.544	.0546	.1313	.158
B-1U	-.053	.035	.391	.0480	.0641	.130	-.064	.034	.418	.0484	.0701	.113
B-2U	-.058	.029	.450	.0488	.0968	.150	-.067	.023	.488	.0529	.1050	.125
B-3U	-.063	.041	.497	.0525	.1233	.160	-.064	.036	.534	.0551	.1364	.130
B-4U	-.047	.059	.518	.0545	.1232	.170	-.056	.057	.545	.0579	.1315	.140
C-1U	-.017	.076	.387	.0483	.0502	.128	-.020	.077	.413	.0509	.0545	.100
C-2U	-.012	.082	.492	.0513	.0748	.128	-.024	.080	.520	.0545	.0808	.110
C-3U	-.036	.068	.522	.0549	.1213	.147	-.055	.065	.559	.0615	.1282	.130
C-4U	-.043	.068	.535	.0576	.1213	.163	-.057	.065	.570	.0618	.1286	.130

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Body defined by equation:

$$b = \frac{x}{15} \left[2 - \frac{x}{45} \right]$$

$$0 \leq x \leq 73.25$$

Strut center-line position	x (in.)
A	45
B	57
C	67.5
Center-line distance	z (in.)
1	5.12
2	6.38
3	8.50
4	10.62

Figure 1. - Engine-body configuration.

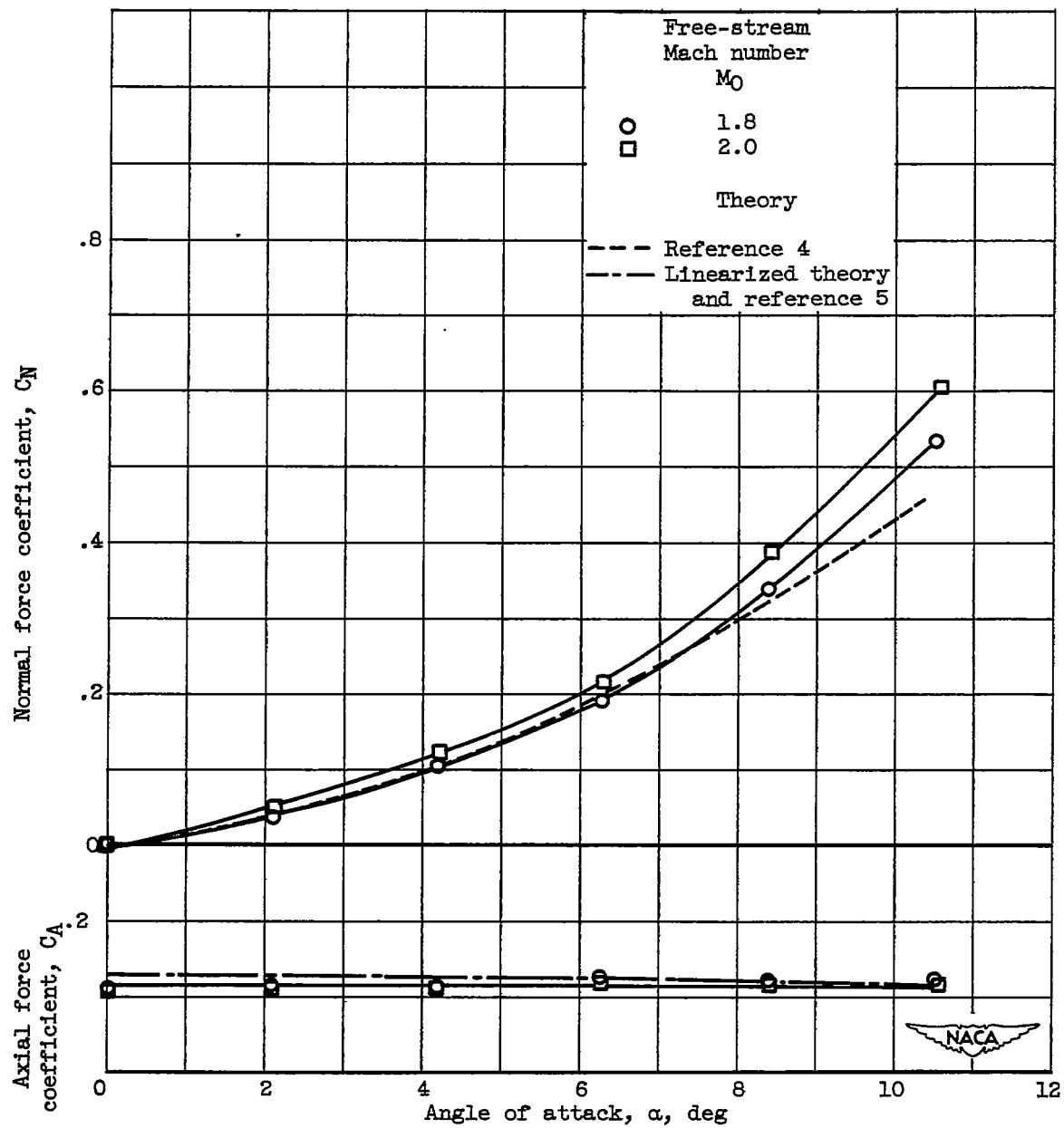


Figure 2. - Characteristics of isolated body.

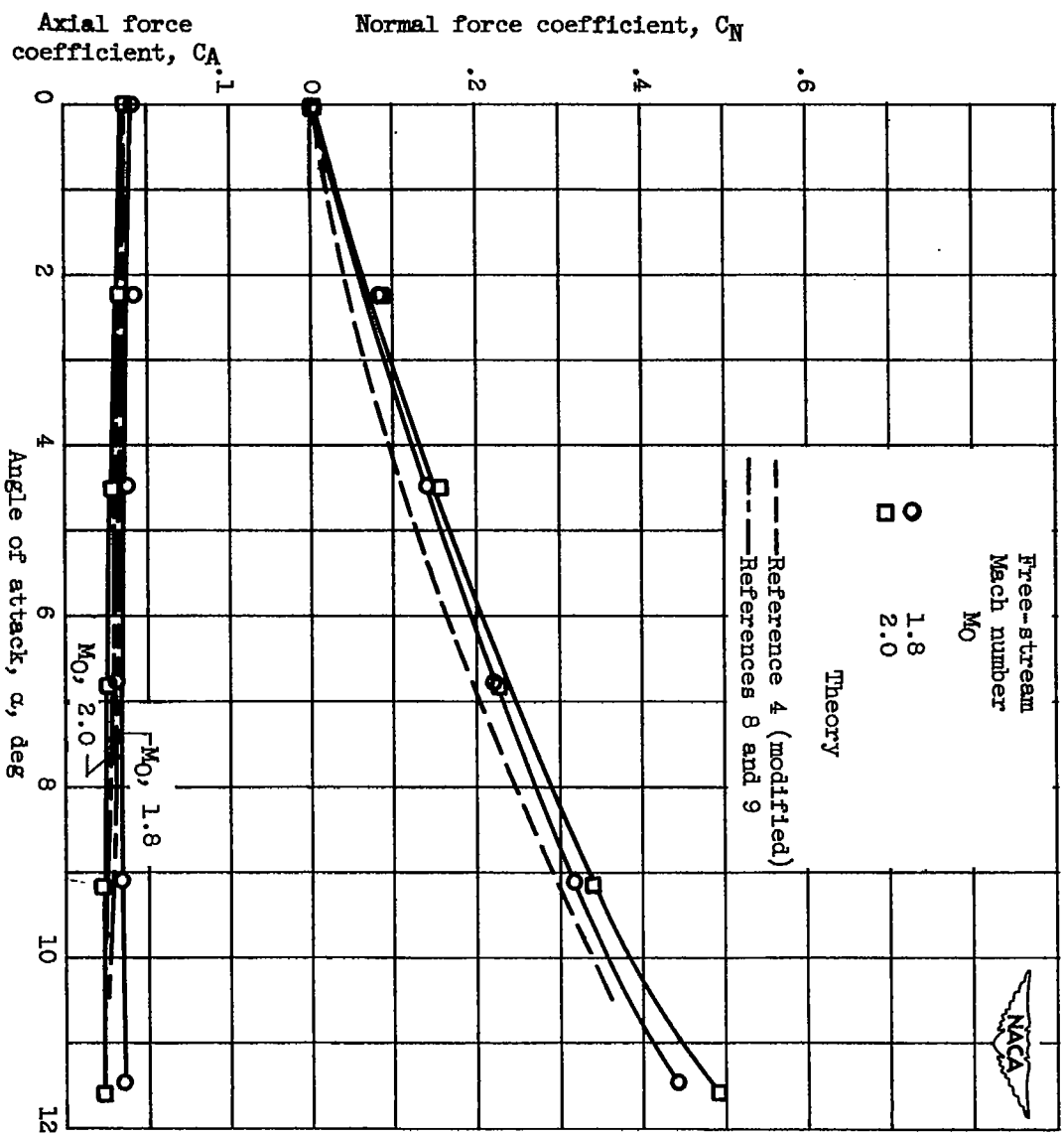


Figure 3. - Characteristics of isolated engine coefficients based on body geometry.

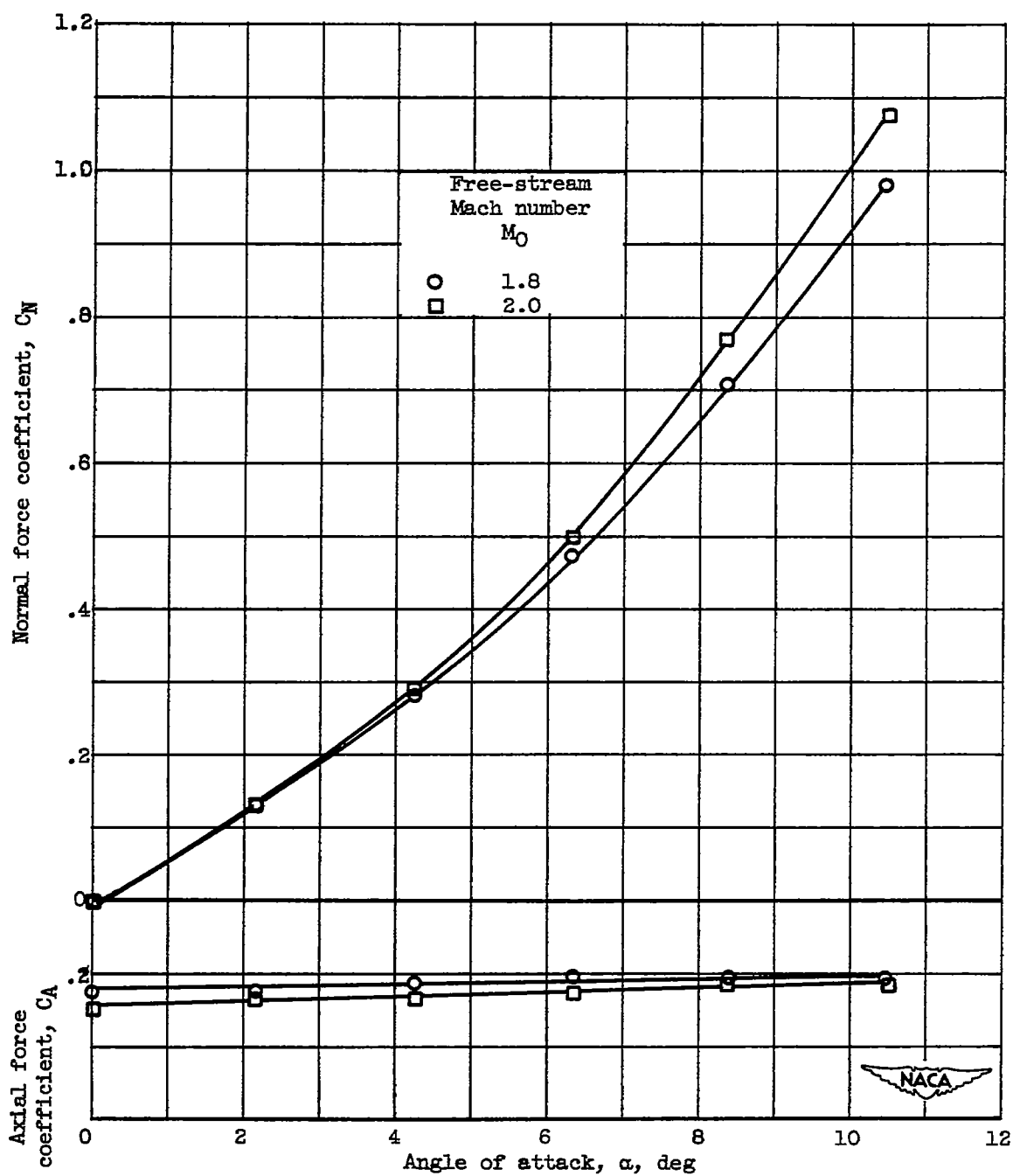


Figure 4. - Characteristics of representative configuration (B-2B).

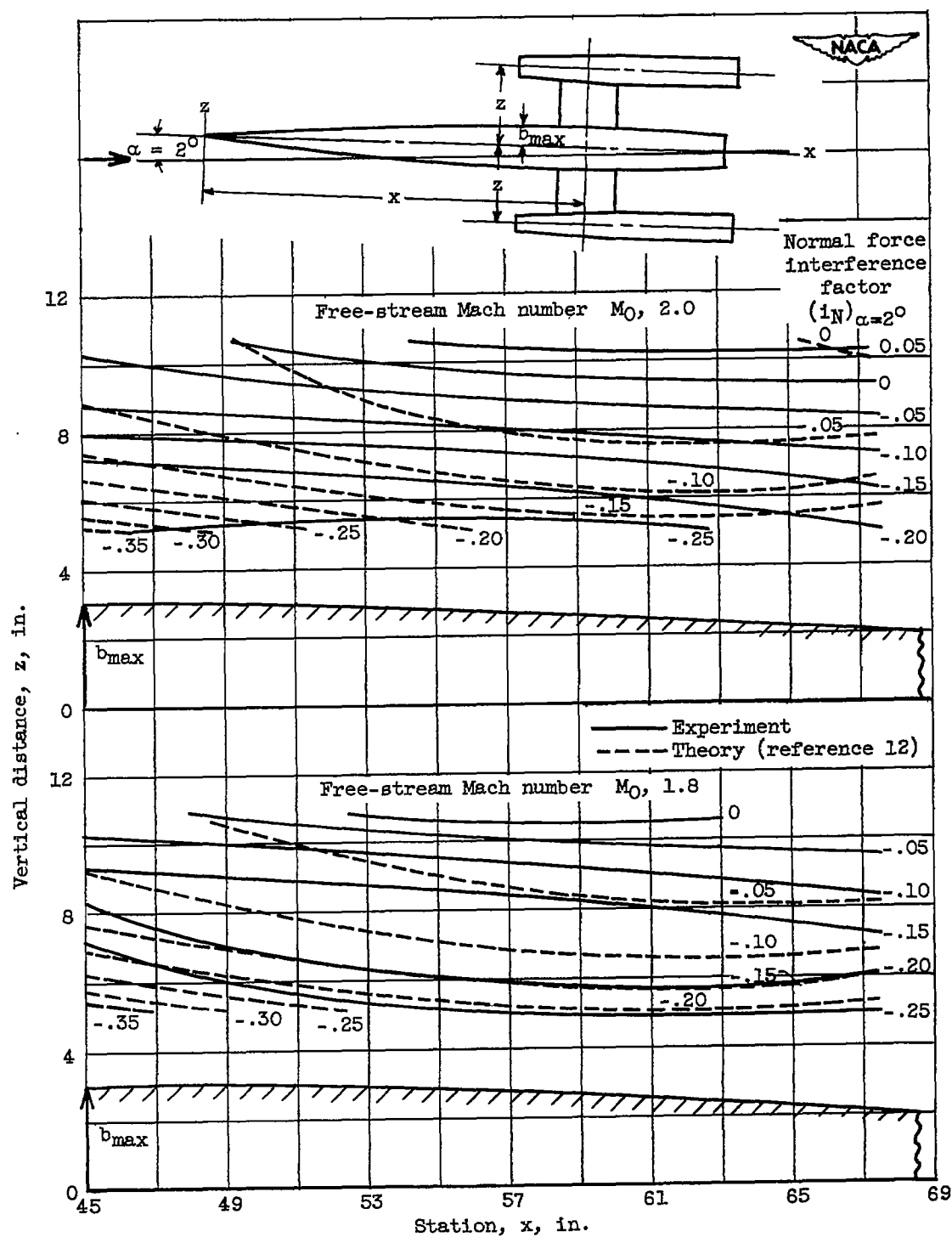
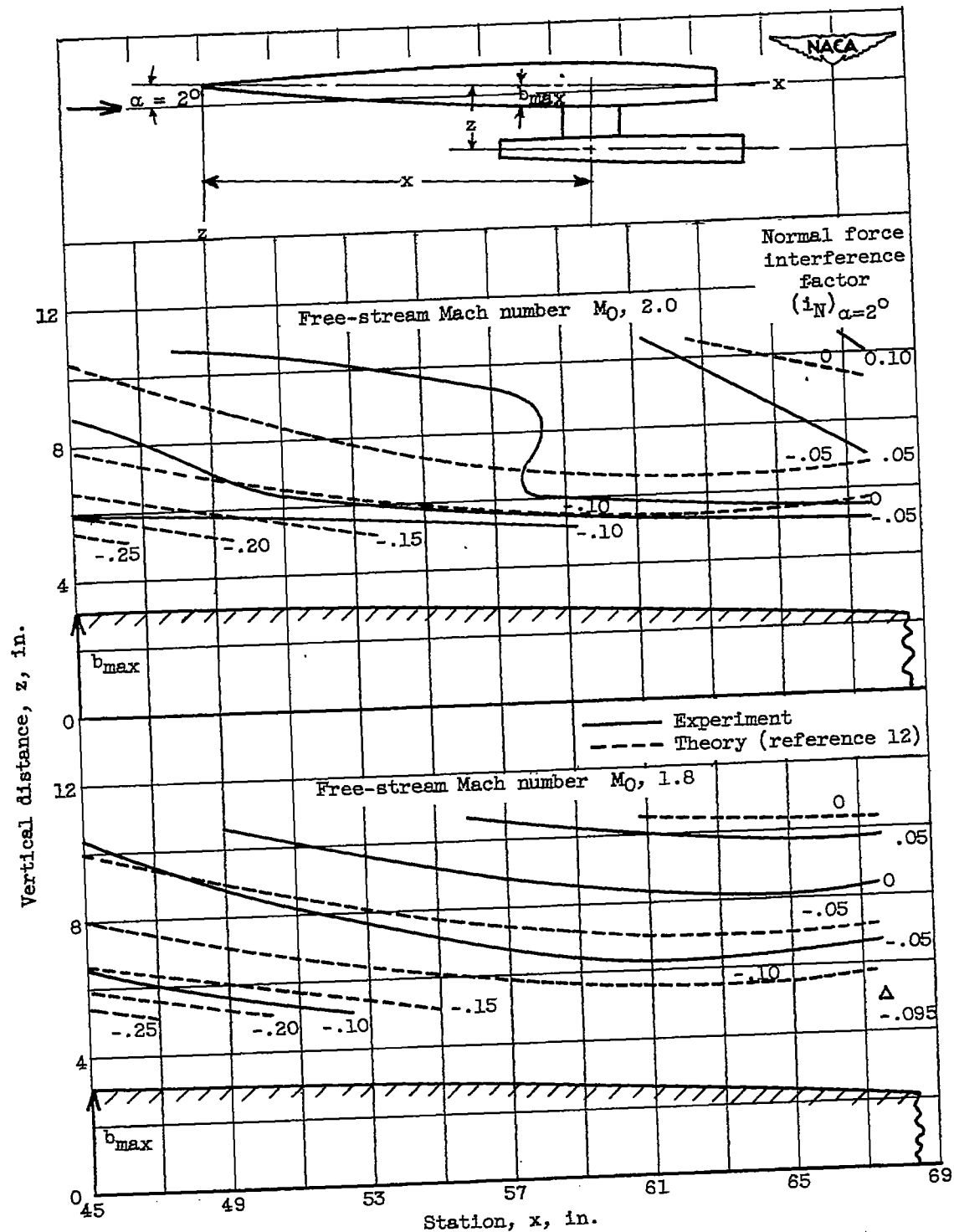


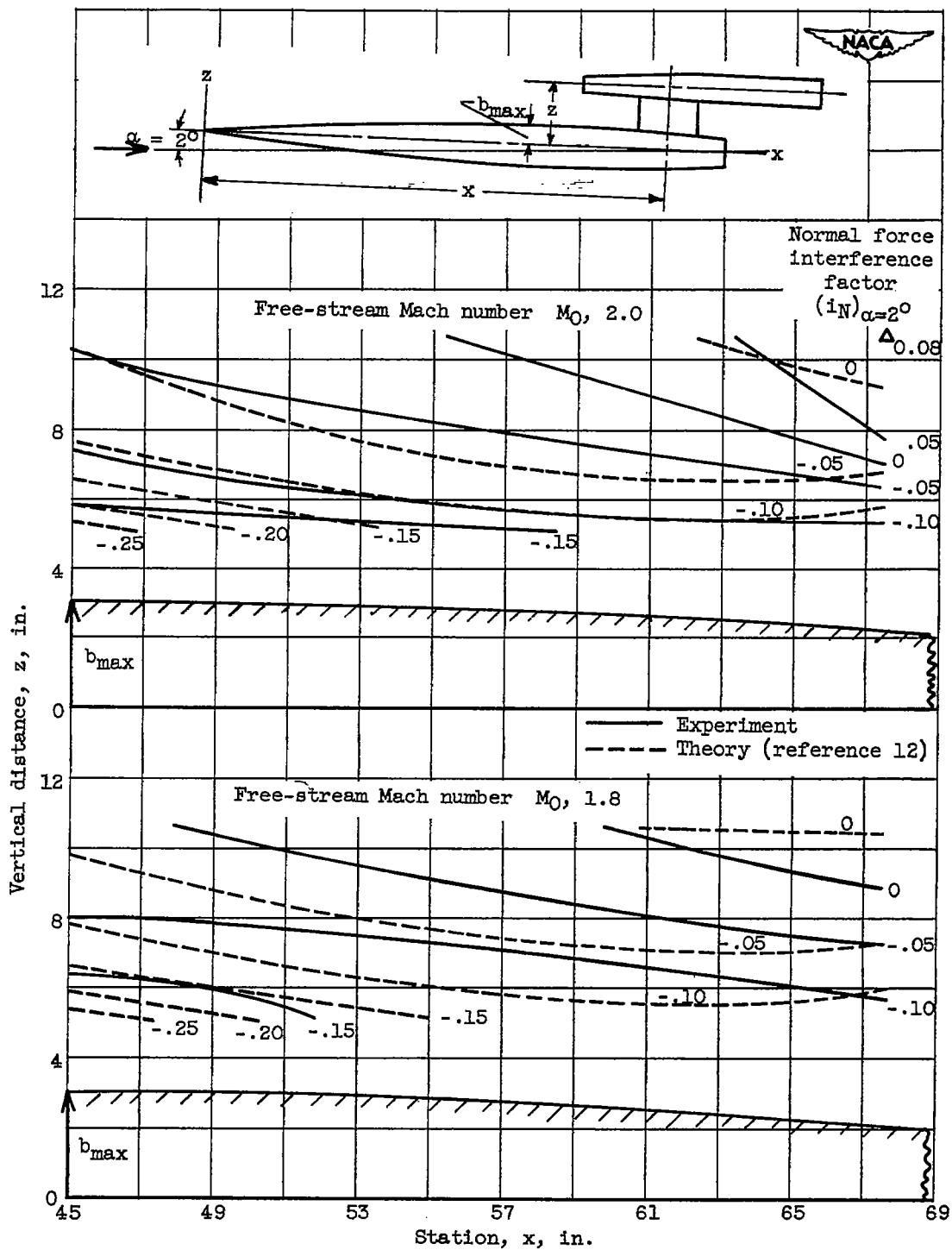
Figure 5. - Contours of normal force interference factor. Angle of attack, 2° .

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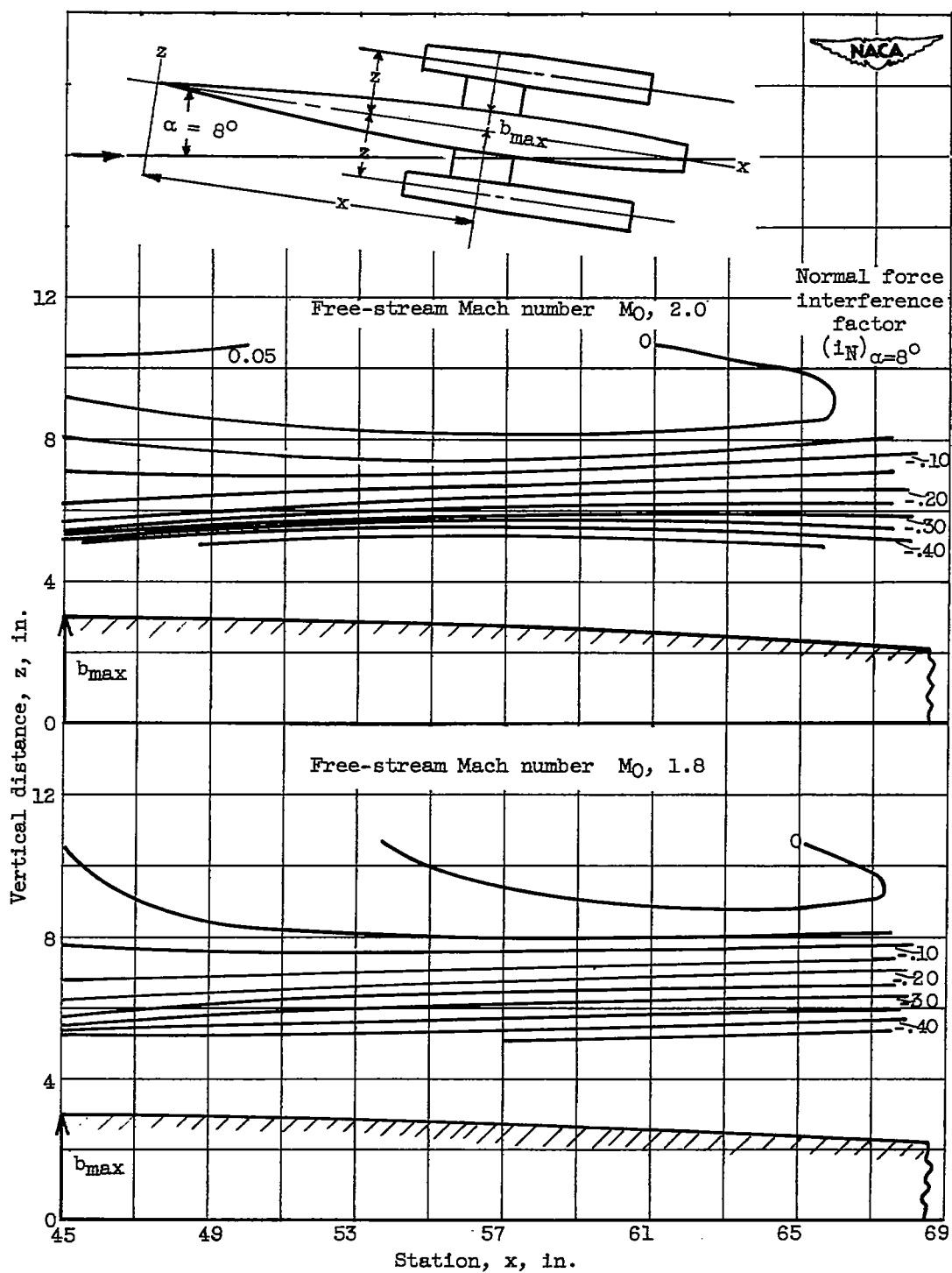
(b) Lower-engine configurations.

Figure 5. - Continued. Contours of normal force interference factor. Angle of attack, 2° .



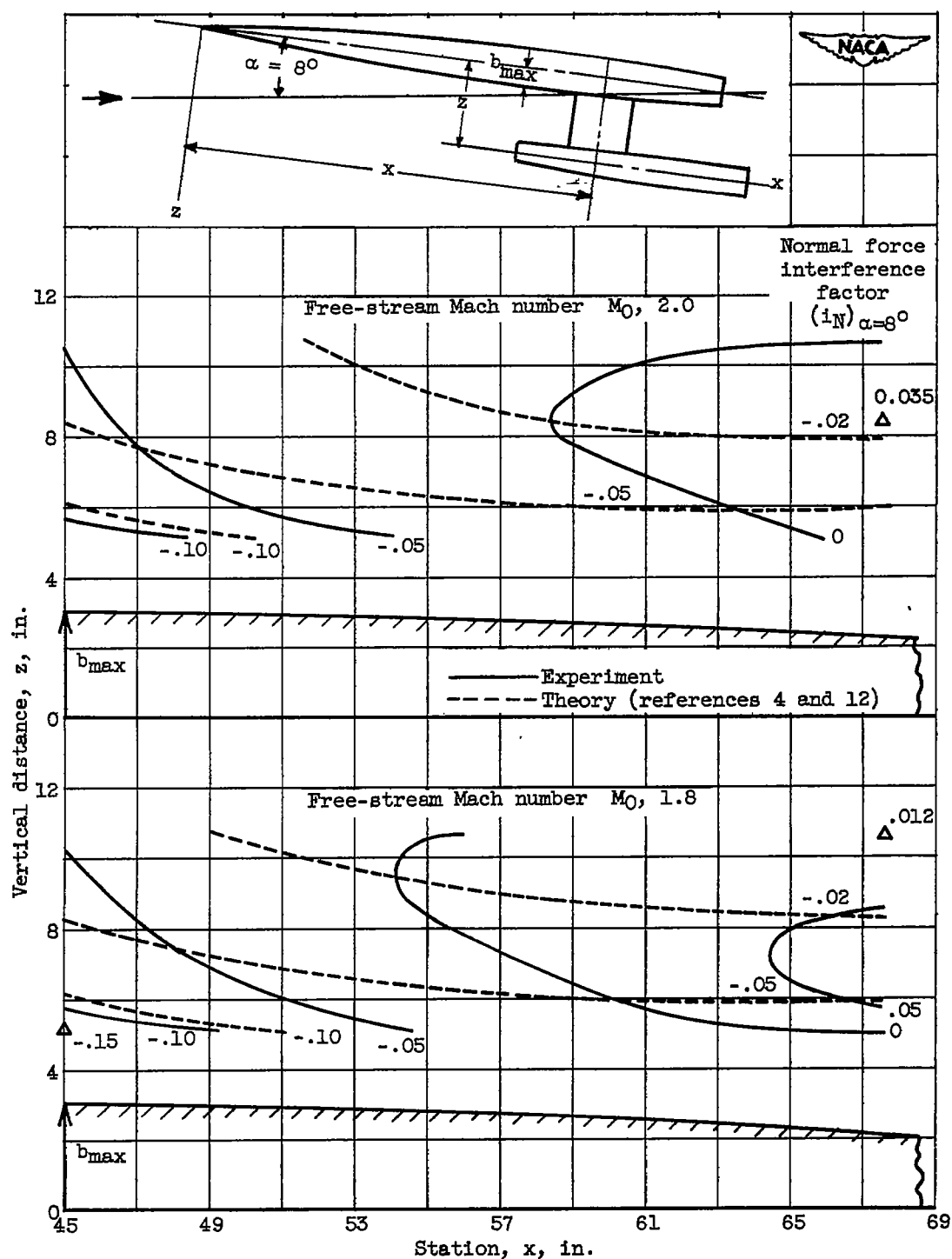
(c) Upper-engine configurations.

Figure 5. - Concluded. Contours of normal force interference factor. Angle of attack, 2° .



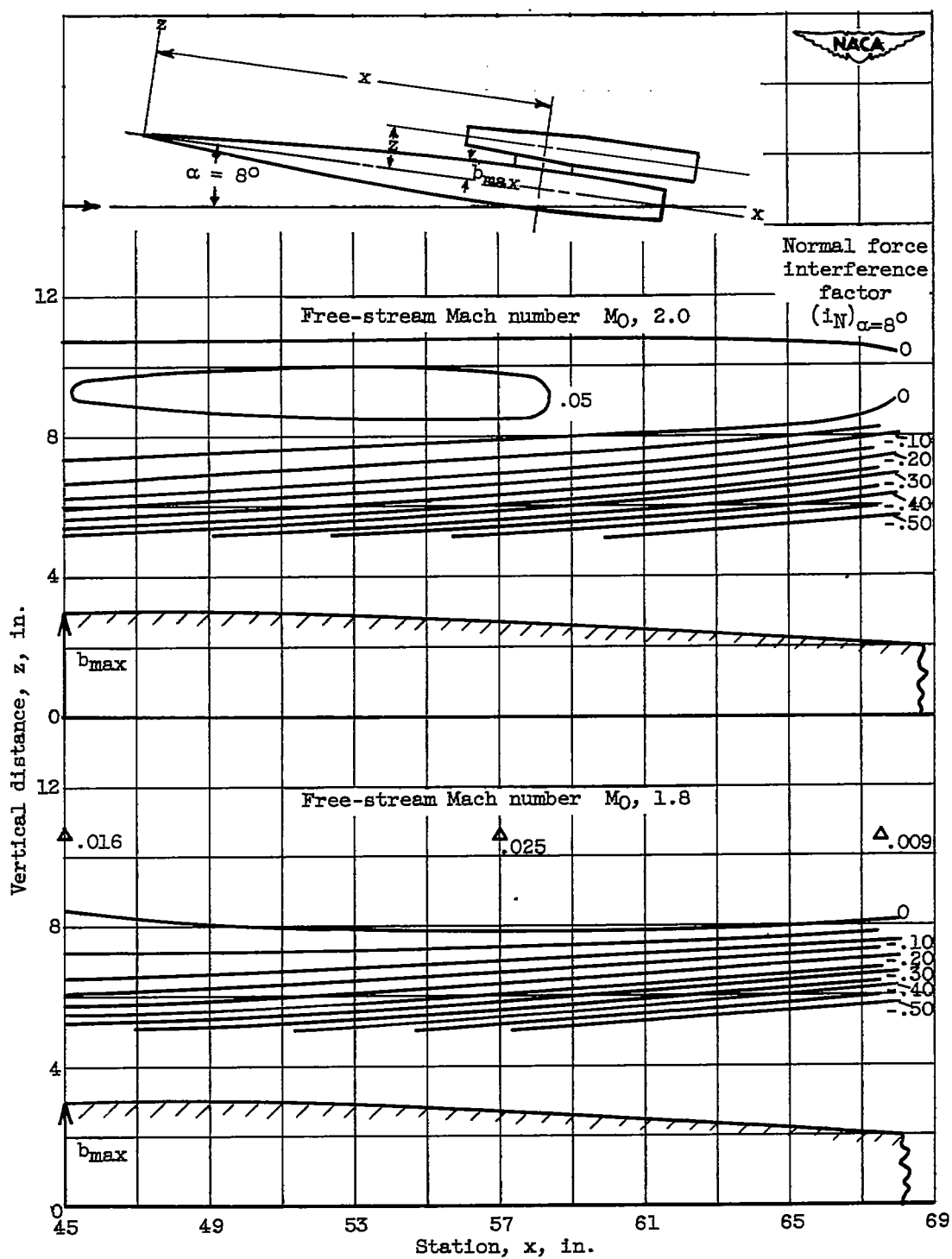
(a) Twin-engine configurations.

Figure 6. - Contours of normal force interference factor. Angle of attack, 8° .



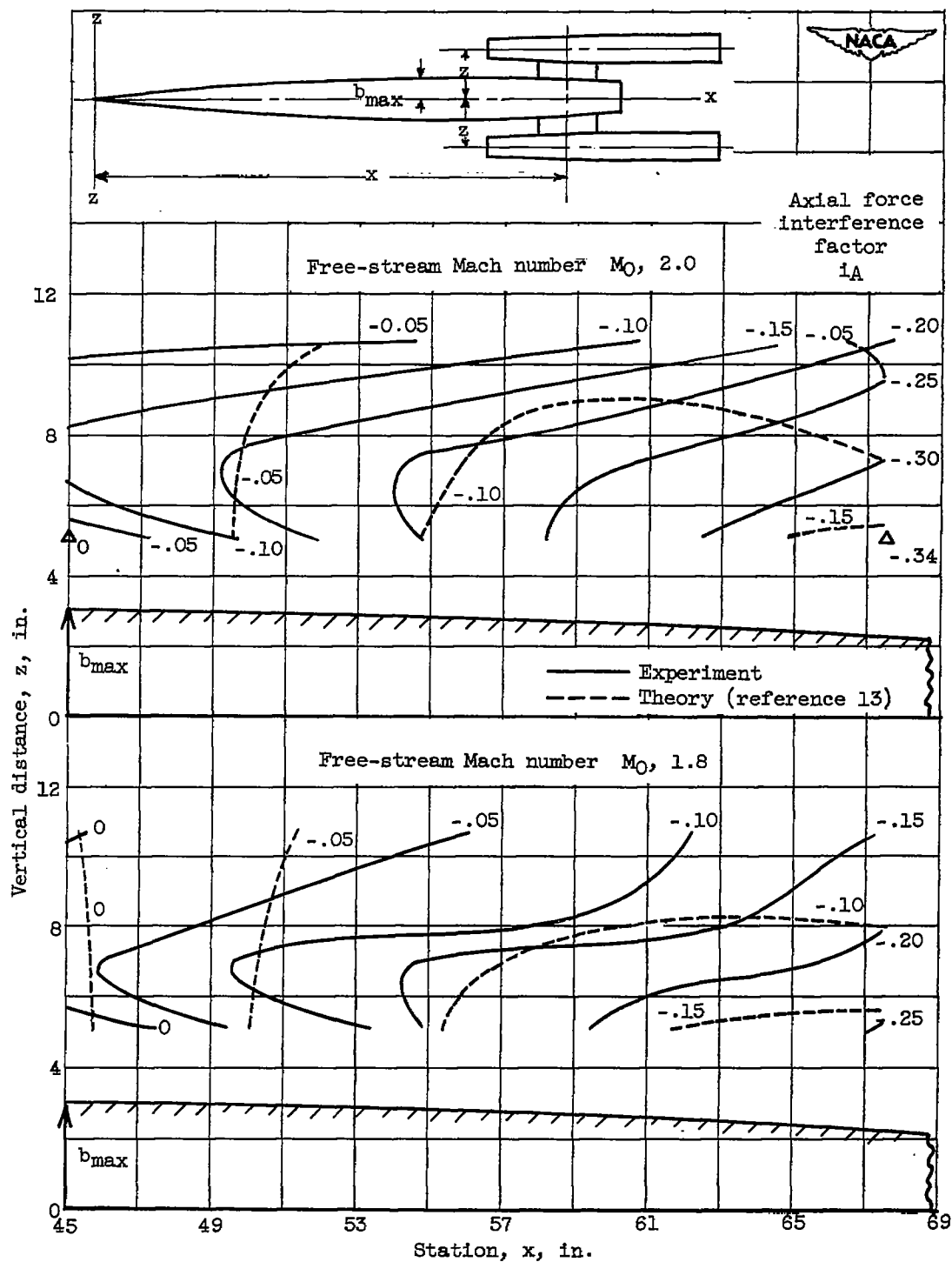
(b) Lower-engine configurations.

Figure 6. - Continued. Contours of normal force interference factor. Angle of attack, 8° .



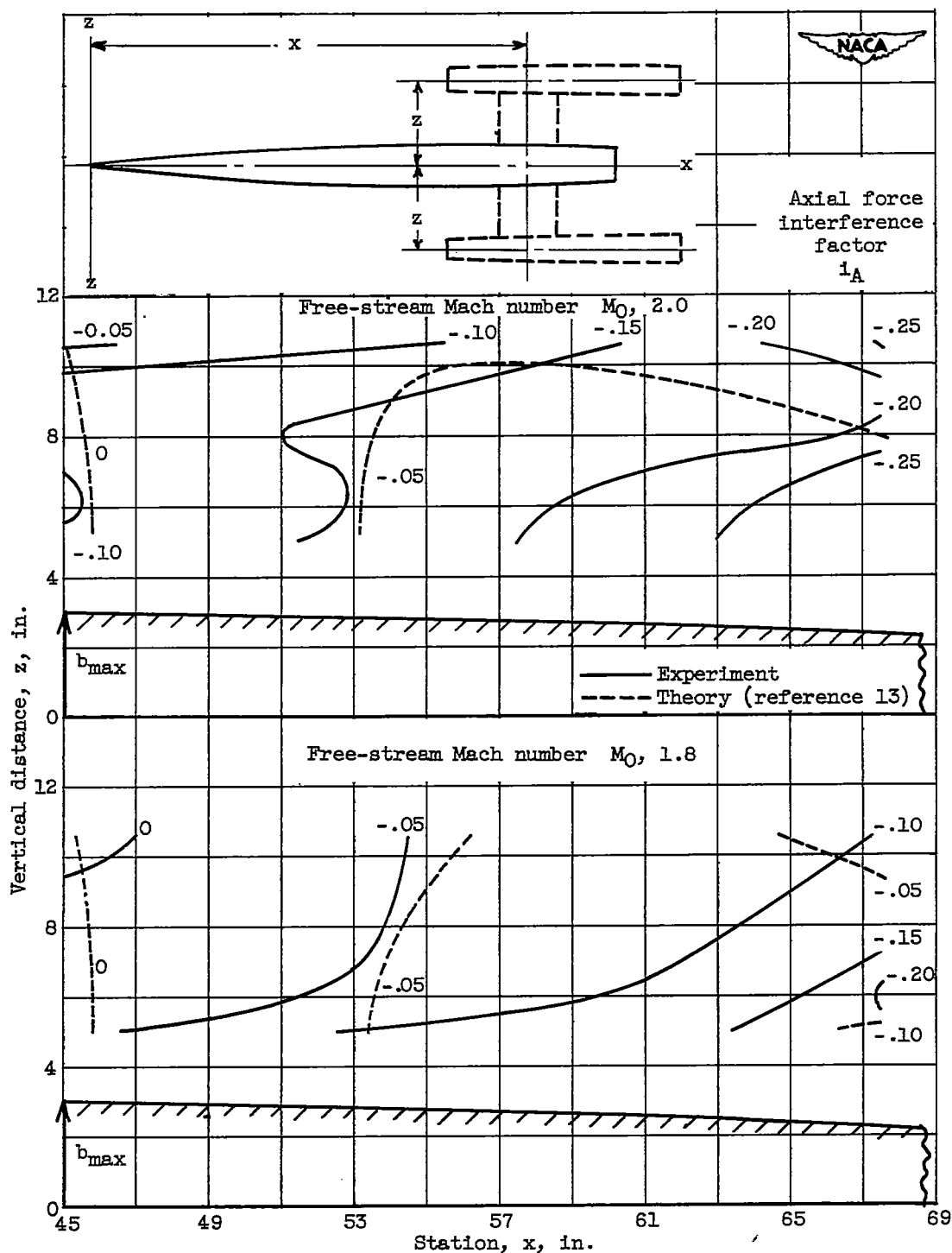
(c) Upper-engine configurations.

Figure 6. - Concluded. Contours of normal force interference factor. Angle of attack, 8° .



(a) Twin-engine configurations.

Figure 7. - Contours of axial force interference factor. Angle of attack, 0° .



(b) Single-engine configurations.

Figure 7. - Concluded. Contours of axial force interference factor. Angle of attack, 0° .

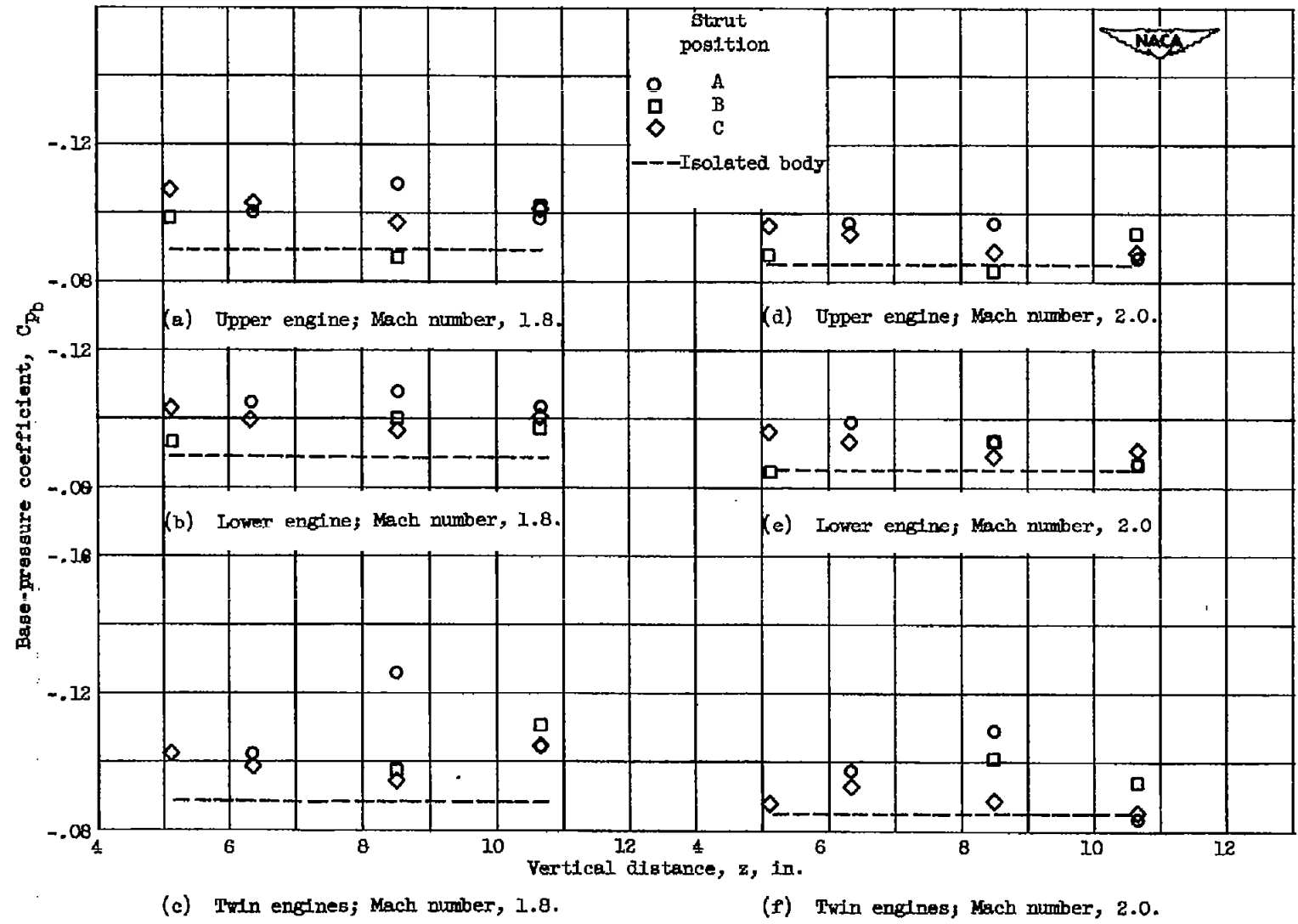
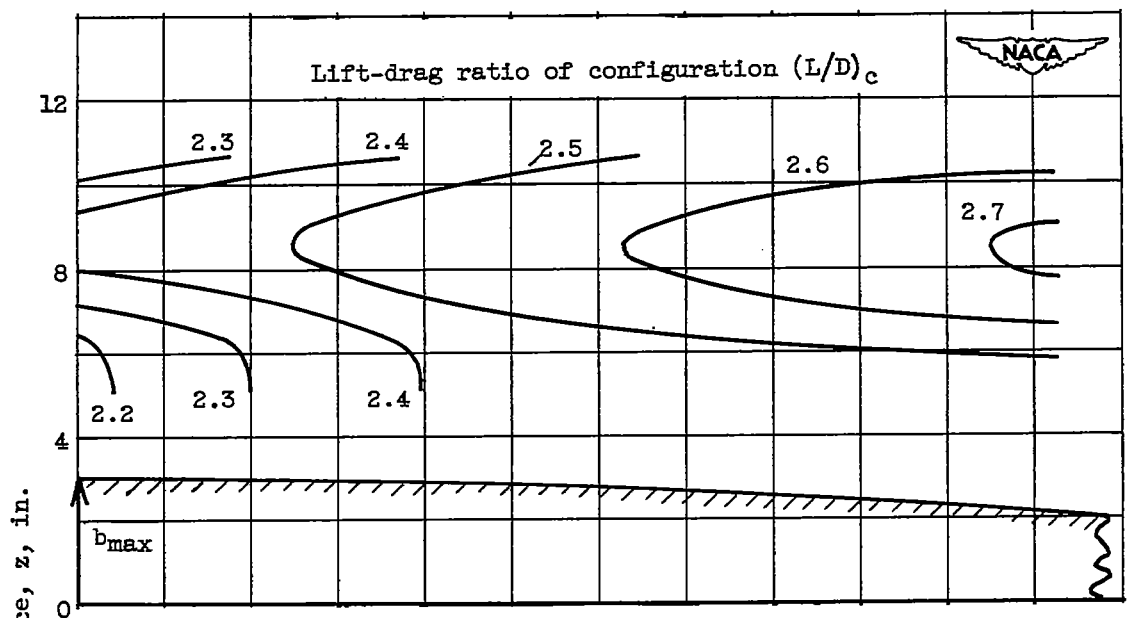
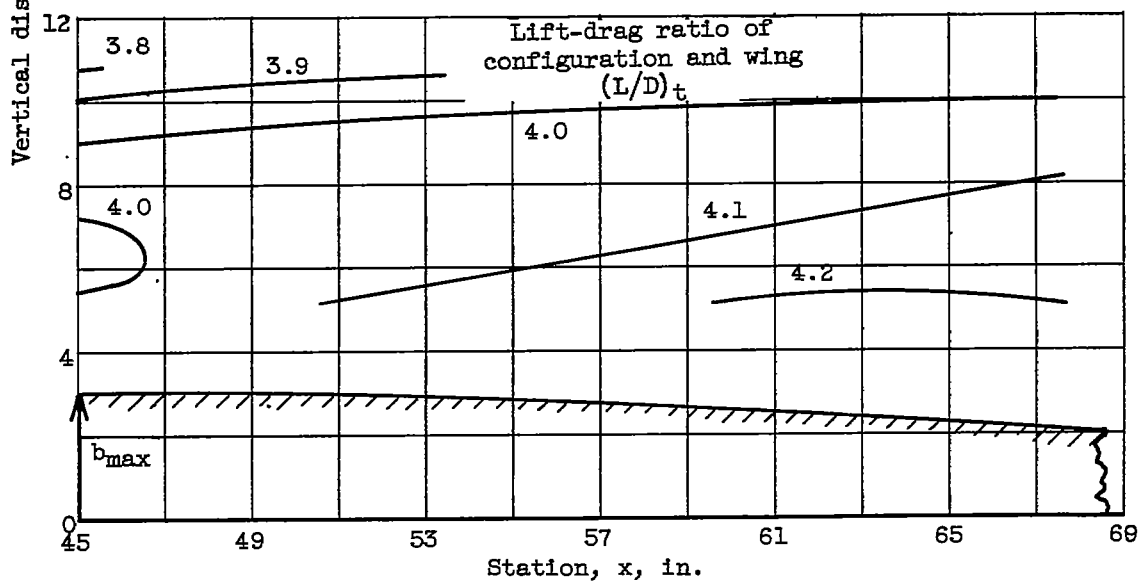


Figure 8. - Body base-pressure coefficient as function of strut length. Angle of attack, 0° .



(a) Configurations alone.



(b) Configurations with wing.

Figure 9. - Contours of lift-drag ratio for twin-engine configurations. Mach number, 2.0; angle of attack of configuration, 8° ; lift-drag ratio of wing, 5.0.

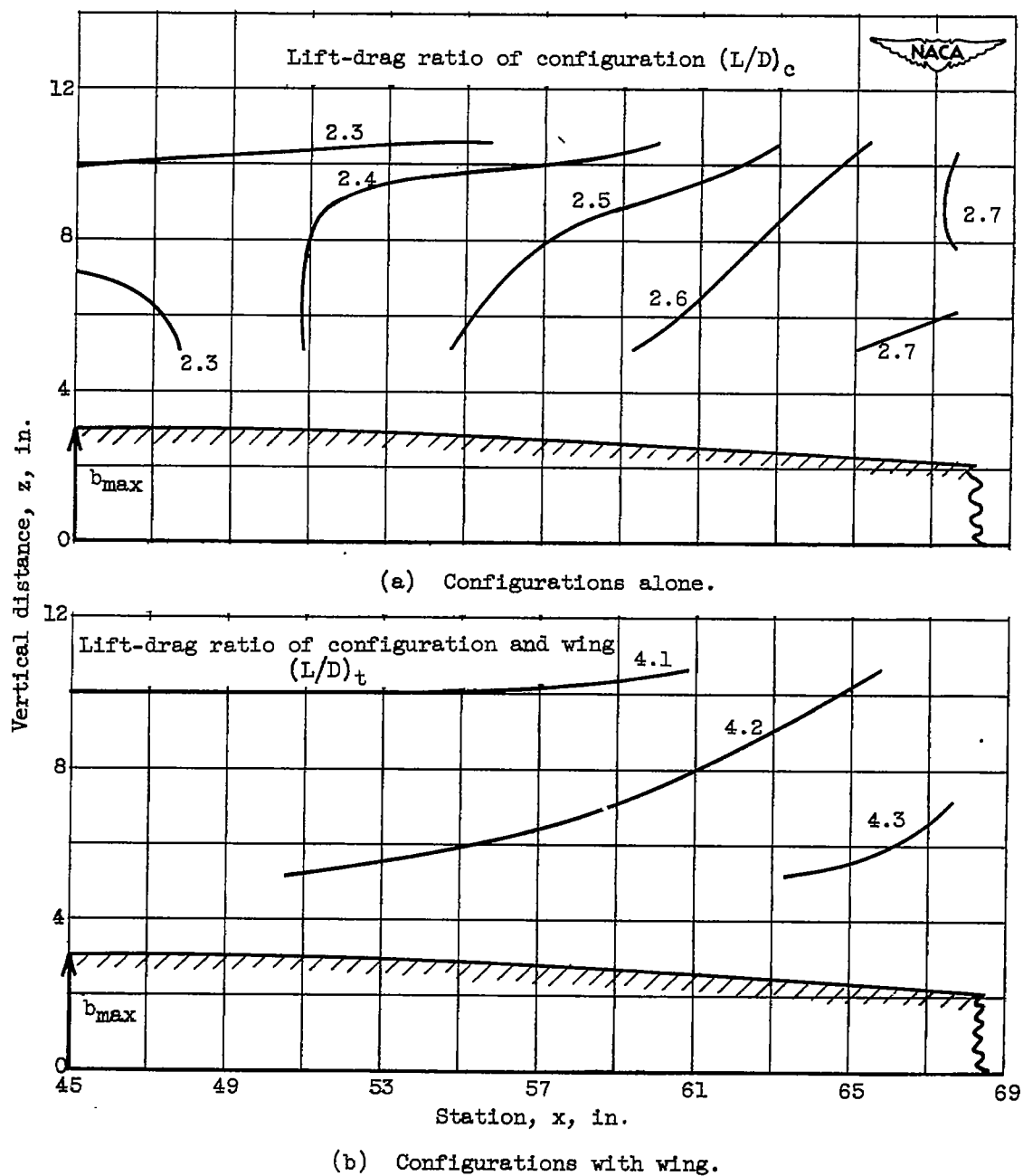
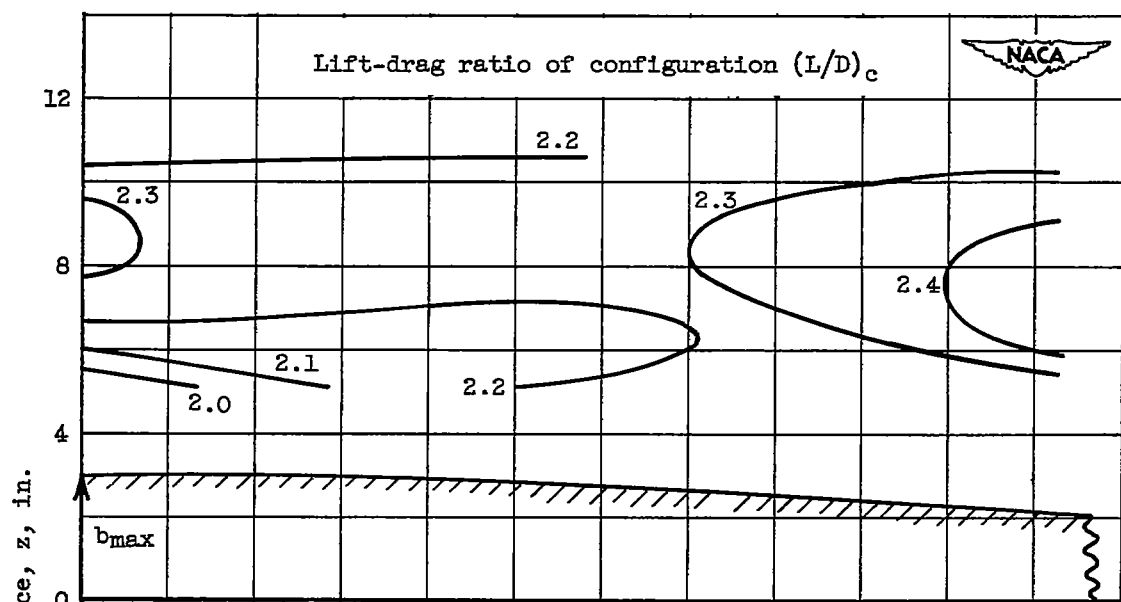
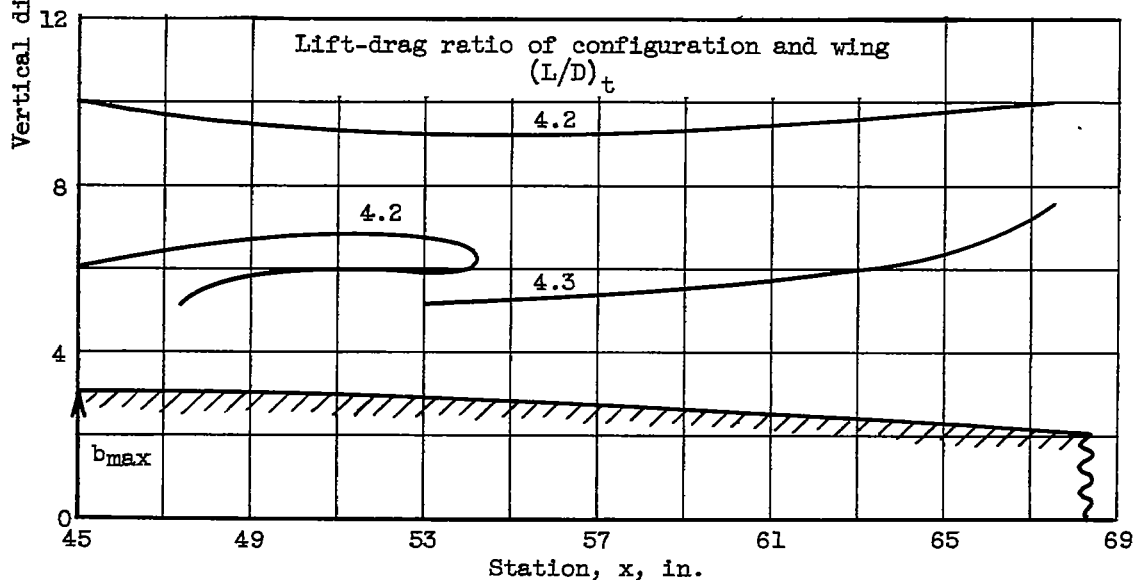
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Figure 10. - Contours of lift-drag ratio for lower-engine configurations. Mach number, 2.0; angle of attack of configuration, 8° ; lift-drag ratio of wing, 5.0.

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(a) Configurations alone.



(b) Configurations with wing.

Figure 11. - Contours of lift-drag ratio for upper-engine configurations. Mach number, 2.0; angle of attack of configuration, 8° ; lift-drag ratio of wing, 5.0.